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DEVELOPMENT CONCEPT FOR A SMALL, SPLIT-CORE, HEAT-PIPE-COOLED NUCLEAR REACTOR

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16. Abstract

There have been two main deterrents to the development of semiportable nuclear reactors. One is the high development costs; the other is the inability to satisfy with assurance the questions of operational safety. This report shows how a split-core, heat-pipe cooled reactor could conceptually eliminate these deterrents, and examines and summarizes recent work on split-core, heat-pipe reactors. A concept for a small reactor that could be developed at a comparatively low cost is presented. The concept would extend the technology of subcritical radioisotope thermoelectric generators using $^{238}PuO_2$ to the evolution of critical space power reactors using $^{239}PuO_2$.

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SUMMARY

There have been two main deterrents to the development of semiportable nuclear reactors. One is the very high reactor development costs; the other is the inability to satisfy the requirements of operational safety. An approach that could ease reactor development is the selection of a split-core, heat-pipe cooled reactor design capable of reasonably high power that is compatible with low power radioisotope thermoelectric generator (RTG) requirements. Key components could first be developed and used in an advanced RTG to establish their reliability. The developed components may then be used in a split-core reactor, thereby considerably reducing reactor development costs. With regard to the safety of this reactor, the fabrication and assembly of only one-half of a core at a time would eliminate the possibility of a criticality accident. Then, the two halves of the system could be sent into space one at a time for assembly in orbit. Thus, many of the present launch safety procedures for RTG's should be applicable to the subcritical assemblies.

This report examines and summarizes some of the recent work on split-core, heat-pipe reactors. It presents a design concept for a small reactor that could be developed at a reasonable cost.

INTRODUCTION

Electric power supplied by the present solar-cell battery systems or radioisotope thermoelectric generators (RTG) is expensive. For these systems the unit cost of electrical energy will not diminish with increasing powerplant size as for reactors (ref. 1) but instead will rise. This rise in cost for solar-cell systems results from the problems of structural support, solar orientation, and radiation pressure that become more severe with increasing size. For the RTG, the main cost is the heat producing isotope so that the unit cost of electrical power is independent of system size. The safety and handling problems of RTG's increase with size so that unit power costs for large systems

also tend to increase.

As RTG systems fueled with plutonium-238 dioxide ($^{238}PuO_2$) increase in thermal power, a size will be reached at which the system will operate as a critical ^{238}Pu reactor (ref. 2). At criticality, the thermal power can be increased greatly by fission of the loading of $^{238}PuO_2$; therefore, the unit cost of electric power when operating in the reactor mode should be reduced significantly. Even though a reactor control system and a radiation shield would be required for the reactor system, low unit electric power cost would result because about 50 times the thermal power could be generated in the reactor mode of operation than in the RTG mode. Further cost reductions could be achieved by the reactor approach since the fuel would not have to be ^{238}Pu but could be the relatively inexpensive ^{239}Pu (ref. 3).

Although the costs of reactor produced electric power may be relatively low on a per unit basis, the development costs associated with the limited production of these power plants may be excessive. There is little doubt that the development of semiportable nuclear reactors along traditional lines can be costly. A review of past nuclear power projects discloses that the major costs in the development arise from uncertainities in the behavior of materials at the desired operating conditions. A minimum cost reactor development program, then, should use the existing information on nuclear materials and adapt the reactor design so that the operating conditions approach acceptable material limits. Since many of the pertinent materials limits have been established in the present RTG program, the use of these materials would be a major step in the reduction of reactor development costs. As for the fuel material, the only change from RTG materials would be in using a critical loading of $^{239}PuO_2$ to replace the thermochemically identical $^{238}PuO_2$. Thus it appears that a reactor development could start with materials that are compatible at the required temperatures, though some material development may be necessary. Neutron and gamma radiation limits for the reactor development would be established in a minimum size reactor under actual operating conditions.

Many questions arise regarding the operational safety of a small, semiportable reactor. However, the known safety requirements and experience for a large RTG would serve as a base from which to approach the safety problems for a space reactor. Options to reduce risks for a reactor in space can be generated regardless of how stringent the safety rules may be. One such option is to use the split-core reactor concept (refs. 4 to 6). With this concept only one-half of the system need be sent into space at a time, so that the chance of a reactivity accident would be as remote as for a large RTG. After two halves are delivered to the space orbit where the system is to operate, they could be assembled in a prescribed manner for startup and operation.

The method of assembling a reactor core from two halves has been used extensively for very low power "critical" experiments (refs. 7 to 9). For fast-spectrum reactor critical experiments, such as the Argonne National Laboratory's ZPR series, the split

table assembly is almost a standard design.

But, until the reintroduction of the heat-pipe heat-transfer concept (ref. 10), it was hard to conceive of how a split-core design could be used for a space power reactor. The combination of a split-core, that is heat-pipe-cooled provides a compact reactor concept for which there are two interacting design areas that should be investigated. One is the design and operation of a split-core reactor to produce power; the other is the effect of heat-pipe cooling on stability of reactor operation. It is these two areas that are examined in this report in support of the evolution of the space power reactor concept.

USE OF ADVANCED RTG TECHNOLOGY FOR AN INITIAL REACTOR

Because RTG materials are similar to those that could be used in a fast-spectrum reactor, it appears that an efficient, economical way to fulfill future space power requirements would be to develop an RTG that eventually could evolve into a reactor system. In this way, the competitiveness and usefulness of RTG's would be enhanced, and the key components of the small, fast-spectrum reactor would have the benefit of operational experience in space.

The conceptual first step for such a program is shown in figure 1. The RTG design starts with an iridium-coated molybdenum capsule containing PuO_2 fuel wafers. Seven of these capsules fit tightly into holes drilled in a 14-centimeter-diameter cylinder made up of molybdenum disks (fig. 1(a)). Twenty-four cylindrical heat pipes transfer the heat to a heat exchanger section, which contains seven large-diameter heat pipes instead of fuel capsules (fig. 1(b)). Four of the large heat pipes assist in the transfer of heat to the other three, which lead to the hot junctions (1353 K) of the axisymmetric silicon-germanium thermoelectric compression modules. The cold junctions (743 K) are cooled by heat-pipe radiators (e.g., figs. 1(b) and (c)). This small system could produce 160 electrical watts from 2500 thermal watts of $^{238}PuO_2$. This RTG system would have in it the key components needed for a small fast-spectrum reactor. As will be discussed later, the diameter and weight of the energy source can be reduced by altering the shape of the small heat pipes located between the fuel capsules. But the design shown in the figure may be an acceptable compromise between performance and simplicity.

There are, of course, several major differences between a RTG and a nuclear reactor. One of these is the radiation shielding requirement. The amount of neutron and gamma shielding required has proven to be a major deterrent to the development of semiportable nuclear reactors. Studies on the ANP, 710, SNAP-50, SNAP-8, APR, and thermionic reactor programs have shown that the only way to minimize nuclear reactor shield weight is to minimize the size of the reactor core. The results of one study are

given in reference 11 from which the curve of figure 2 was obtained. This curve shows the shield thickness scaling factor as a function of power level of the SNAP-8 reactor. It shows that with constant core size the required increase in shield thickness for a factor of 2 increase in reactor power is only 9.5 percent. And it shows that the required increase in shield thickness for a factor of 4 increase in reactor power is only 12 percent. Since the shield size and weight are a direct function of the size of the core, these facts show the importance of minimizing core size and operating the reactor at as high a power level as possible.

The first requirement to achieve minimum core size is to use a fast spectrum reactor. This was showm in references 12 and 13. The second requirement for minimum core size is to maximize the fuel loading. Although the small diameter cylindrical heat pipes provide a reasonable fuel loading, changing to a trifulte shape (fig. 3) provides better utilization of the space in the core and consequently a smaller core size. The triflute heat pipes in this concept would operate at a high enough vapor pressure to help maintain good thermal contact with the fuel elements. The thermal capacity of the heat pipes is acceptable because of their short length (19 cm). The heat-pipe heat exchanger in this reactor adaptation of the RTG components also serves as an axial neutron reflector by the inclusion of cylindrical molybdenum inserts (fig. 4). Calculations show that enough power can be taken from 19 of these fuel elements to generate 5 kilowatts of electricity with silicon-germanium thermoelectric converters. A plan view of the reactor would be that shown in figure 5. The neutronic calculations reported in appendix A show that there is ample reactivity available for criticality and power production for both the triflute and cylindrical heat-pipe-cooled cores. The cores would have a loading of less than 40 kilograms of plutonium (95 percent ^{239}Pu and 5 percent ^{240}Pu).

The triflute shaped heat pipes minimize the core size and hence the shield weight. However, there are probably some, partially shielded, unmanned missions where minimum size and weight would not be required. The difference in system weight for triflute and cylindrical heat pipes may not warrant the more complex heat pipe shape. Thus, it appears that the first step should be to develop the system with cylindrical heat pipes (as analyzed in appendix A) and then switch to the triflute heat pipes only when necessary.

DESIGN REQUIREMENTS FOR STABLE OPERATION OF A SPLIT-CORE POWER REACTOR

The main difference between the operation of a critical assembly and the operation of a power reactor is that a critical assembly produces an insignificant amount of heat.

Startup and control aspects are similar for both. For the power reactor, fuel elements generate heat that is transferred to a coolant and materials expand with the increased temperatures and temperature gradients in the reactors. Bending and bowing of fuel elements can result. Thus, in power reactors fuel movements are expected that have not had to be considered in split table critical assemblies.

Avoidance of excessive temperatures in nuclear reactors depends on limiting the magnitude and insertion rate of positive reactivity. In a split-core reactor positive reactivity is inserted by bringing the two core halves closer together. Thus, the safety of this concept depends on the ability to limit the rate at which the nuclear fuel in the two core halves is brought together and to predict all subsequent movements of fuel.

One of the advantages of using a split core is that it offers a large amount of reactivity control without grossly altering the power distribution over the life of the reactor. This favorable design feature implies that it is also possible to achieve a high rate of reactivity insertion. It is necessary that the rate of insertion be physically limited. For example, a characteristic split-core control curve is shown in figure 6 (from ref. 14). The curve shows that, for a reactor with a built-in excess reactivity of 2 percent (required to satisfy temperature reactivity effects and core life at power), a 10-centimeter increase in the separation of the core halves provides a 18-percent shutdown margin. But for gap thicknesses less than 3 centimeters the reactivity increases almost linearly with decreasing gap thickness at the rate of 3 percent per centimeter.

The curve for uranium-233 (^{233}U) fuel was used as an example because neutronically it is similar to ^{239}Pu. Both fuels would allow for the design of smaller reactor cores than with ^{235}U; but both ^{235}U and ^{239}Pu have smaller delayed neutron fractions (β_{eff}) than ^{235}U (see table I; from ref. 15). The delayed neutron fraction β for ^{233}U and ^{239}Pu is about one-third of that for ^{235}U. Neutron kinetics of ^{239}Pu and ^{233}U fueled reactors should, therefore, be similar. The primary effect of the lower β_{eff} is to increase the sensitivity of a given control device. Thus, in a study of the kinetics of a split-core reactor (ref. 16), where it was found that there is a definite critical rate above which reactivity input rates can result in core melting, the insertion rate limit for a ^{233}U fueled reactor was about 2 cents per second. For a ^{235}U reactor it was found to be 8 cents per second. Thus for a ^{239}Pu core with a reactivity rate of 3 percent per centimeter and a similar 2-cent-per-second limiting reactivity input rate, the closure rate of the two core halves would have to be limited to 0.013 millimeter per second (0.5 mil/sec). Also, it appears that there should always be a positive safety lock which makes the accidental sudden insertion of more than 0.80 dollars ($0.20 less than prompt critical) physically impossible. At 3 percent per centimeter this would mean limiting step changes in the available closure distance at any one time to less than 0.5 millimeter (20 mils). One positive method by which this could be done is sketched in figure 4 of reference 5.

INVESTIGATION OF CONCEPTUAL DESIGNS

AND OPERATING MODES FOR SPLIT-CORE POWER REACTORS

We have calculated that a 3-percent-per-centimeter closure rate for a split core reactor using ^{239}Pu fuel is equivalent to 1.50 dollars per millimeter. This is for the two core halves moving together. But the inadvertent movement of even a small part of the core toward the center of the assembly would have to be limited to a small fraction of a millimeter in order to keep the reactivity increase rate to less than 2 cents per second to avoid a core melting accident.

One positive way of preventing the two core halves and their fuel from coming any closer together than a prescribed amount is to use a shim plate (fig. 7). This plate would be permanently attached to one of the core halves. The thickness of the plate would be preset to give the reactor a small amount of excess reactivity, which would be controlled by other means (i.e., reflector control rods or drums). The control devices could be designed to keep the maximum possible positive reactivity rate at less than 2 cents per second. With this approach the split-core reactor could be controlled conventionally without the possibility of the fuel moving toward the central plane. An example of the shimmed halves with a rotating segment control is shown in figure 8. Separate drive motors on each half could allow the two halves of the system to be taken into space on separate launches. Since one half of a core would be subcritical by a large margin, the separate launches eliminate the possibility of a criticality accident. For a split-core mode of operation, wherein the reactor would be controlled by the accurate positioning of the two core halves (as reported in ref. 5), it should be possible to prevent fuel movement toward the central plane by using a thick enough end cladding on each of the individual fuel elements. This means of control should also adapt to separate launch of the reactor halves.

CHARACTERISTICS OF HEAT-PIPE REACTORS

Although heat pipes have not demonstrated the life times desired for space power reactors, the limited data available have shown satisfactory performance for 10 000 hours at 1773 K. Much longer life times should be obtainable with the 1373 K operation projected for the RTG and early reactor applications. It has been shown that very minute quantities of oxygen can greatly increase the corrosion rates. When the reactor is in the vacuum of space, the oxygen corrosion problem should be reduced and the lifetimes of the reactors extended.

A general comparison of heat pipe and pumped coolant (i.e., forced convection cooled) reactors is given in reference 17. The potential advantage of using heat pipes

to cool small reactors, especially for space power, is discussed.

However, another important difference can be seen by comparing the material composition of a heat-pipe reactor with that of an equivalent forced-convection cooled reactor. A forced-convection cooled reactor system capable of producing around 100 electrical kilowatts from 450 thermal kilowatts is described in reference 18. The lithium-7 coolant takes up about 25 percent of the core volume. A heat-pipe reactor concept of about the same electrical power level is described in reference 19. From table I (from ref. 20) the volume fraction of the lithium-7 coolant in the core of the heat-pipe concept is about 4 percent. Thus the lithium volume fraction is reduced by a factor of six in the heat-pipe reactor. In general heat-pipe reactors will tend to need much less coolant than convectively cooled reactors.

Neutronically a coolant in a reactor does two things. It slows down (or moderates) neutrons, which, in general, enhances reactivity. And it parasitically captures neutrons which reduces reactivity. A coolant like lithium-7, which had a very small neutron capture cross section, gives the reactor a negative temperature coefficient of reactivity because, as the temperature of the reactor goes up, the lithium expands and leaves the core. Hence, some neutron moderation and consequently reactivity is lost. For example it is calculated that the reactor of reference 18 would lose 0.4 percent in reactivity when the temperature is raised from 203 to 1480 K. For ^{239}Pu fuel this results in an average lithium-7 temperature coefficient of -0.2 cents per K. This is a very significant coefficient for inherent reactor control and is a direct result of 25 percent of the core volume being lithium-7. But a typical heat-pipe reactor (ref. 19) has a lithium-7 volume fraction of only 4 percent. Thus, heat-pipe reactors will, in general, have much smaller negative reactivity feedbacks from the expansion of the moderator.

INVESTIGATION OF A METHOD FOR ENHANCING POWER STABILIZATION

In reference 21 it is shown that, even with the loss of the negative moderator coefficient of reactivity, heat-pipe reactors can be controllable. This is due to the negative contribution of various other temperature expansion effects on reactivity. The values of these coefficients used in the dynamic analyses of a heat-pipe reactor are given in reference 22. For the ^{233}U reactor analyzed, these total -0.6 cents per K. From this value, it is seen that the loss of a -0.2 cents per K moderator coefficient would amount to a loss of 25 percent of the total negative temperature coefficient; so the reduction of expansion coefficient is not necessarily a major factor in the control of heat-pipe reactors.

From these analyses (ref. 21) it was also found that stable operation of heat-pipe reactors at power is more limited than during startup. Thus, it may be desirable to have a

method for "designing in" an inherent negative power coefficient of reactivity. One possible method for doing this was reported in reference 23. This method takes advantage of the fact that in the split-core concept the control shafts that position the core halves can be placed into a high neutron flux field (fig. 9). The neutron current into the center of the control shafts is enhanced by neutrons streaming from the central control gap. Thus, if nuclear fuel is placed near the shafts, the heating rate in the fuel will be proportional to the neutron flux (i.e., essentially to the reactor power). If the fission heat can be used to make the control shafts expand axially, the reactor could have an additional negative power reactivity coefficient for enhanced stabilization of the steady-state operating condition.

As found by the detailed analysis reported in appendix B, placing fissionable fuel around the control shafts causes the initial shaft heating to be adiabatic and gives the maximum rate of axial expansion. By thermally insulating the fuel the temperature variation with power change can be enhanced. The magnitude of this power coefficient is nearly inversely proportional to the radial distance of the control shafts from the center of the reactor.

Typical results for three different control shaft materials are given in table II. These results are from the analysis in appendix B and reference 24. The power reactivity coefficients are given for a 32-centimeter-diameter uranium-235 carbide (^{235}UC) and a 20-centimeter-diameter ^{233}UC reactor, both with 7-centimeter radial reflectors. These reactors were assumed to be operating at 1 megawatt of steady-state thermal power, and had 10-centimeter-long fueled power shafts. The power shafts were insulated with 2 centimeters of beryllium oxide and placed 10 centimeters from the radial reflector. Being smaller, the ^{233}UC reactor has a higher neutron leakage rate and a less dispersed current 10 centimeters away from the reflector. This fact and the more sensitive gap worth of the ^{233}UC reactor indicate that the ^{233}UC reactor has power reactivity coefficients about four times those for the ^{235}UC reactor. It would be expected that the results for a ^{239}Pu reactor would be quite similar to those for a ^{233}U reactor.

CONCLUDING REMARKS

In regard to the operating stability of a split-core reactor, a positive way to limit excess reactivity of the two core halves as they approach each other is to attach a shim separation plate to one of the core halves. The shim separation plate, in conjunction with conventional control methods should insure stable operation of a split-core reactor.

Another method, based on controlling the separation between the two halves, uses fueled control shafts to achieve reactor stability. Although more analysis and experi-

mental evidence are required, a 10-centimeter-long molybdenum control shaft, insulated with 2 centimeters of beryllium oxide, should provide a negative power coefficient of about 13 cents per megawatt of thermal power in a $^{239}PuO_2$ reactor.

It has also been proposed that the key components of an advanced radioisotope thermoelectric generator could, once developed and tested in space, be used almost directly for an initial space power reactor. Further, this concept could result in a very small core and hence lead to low radiation shield weights that could then provide a substantial performance improvement over competitive space power systems.

Lewis Research Center,
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APPENDIX A

RESULTS OF NEUTRONIC CALCULATIONS FOR THE REACTOR

The cylindrical model shown in figure 10 was used for the four energy group S_4P_1 in calculations with the R-Z option of the DOT computer program. The GAM II program was used to generate the cross sections. The density of the plutonium dioxide pellets was assumed to be 10.08 grams per cubic centimeter. And the plutonium was assumed to be 94.8 percent 239 isotope, 4.9 percent 240 isotope, and 0.31 percent 241 isotope.

This model was derived from an expanded version of the reactor with cylindrical rather than triflute heat pipes. A plan view of this reactor is shown in figure 10. For a plutonium dioxide (PuO_2) volume fraction of 0.582 the neutron multiplication factor k_{eff} was found to be 1.1. Reducing the PuO_2 volume fraction by 10 percent (i.e., to 0.524) reduced k_{eff} to 1.02.

In regard to a more compact core with triflute shaped heat pipes, the calculations reported in reference 25 show that decreasing the core diameter with the same total amount of fuel will cause the reactivity to increase. Thus, there should be plenty of reactivity available for criticality and power production. It may even be possible to make the reactor somewhat smaller.

APPENDIX B

POWER REACTIVITY FEEDBACK OF FUELED CONTROL SHAFT

The split-core reactor can be controlled through the relative position of the two reactor halves. The movement need not be great: for a 32-centimeter-diameter by 26-centimeter-high ^{235}UC reactor, the gap is worth about 36 cents per millimeter; for a 20-centimeter-diameter by 15-centimeter-high ^{233}UC reactor, the gap is worth about 1.04 dollars per millimeter (ref. 26). A control shaft that positions the core halves can be put into a high neutron flux field, which is intensified by neutrons streaming from the control gap. To take advantage of these factors, fuel is added to the control shaft to provide a negative power reactivity feedback. The purpose of this appendix is to determine the magnitude of this effect.

A typical device (fig. 11) may consist of a refractory-metal hollow shaft containing ^{235}UC or ^{233}UC (as per the core) and be insulated by beryllium oxide. The fuel produces heat proportional in quantity to the reactor power. The shaft in turn is heated and expands or contracts according to changes in the reactor power. The refractory metals in consideration, tantalum, tungsten, and molybdenum, all have coefficients of linear expansion much less than that of UC (see table III) so an expansion space is provided at either end of the fueled section. Due to fuel-metal reactions a tungsten fuel liner is necessary for a tantalum shaft. By insulating the shaft the temperature variation with power change is enhanced, and the power reactivity coefficient is higher.

The rate at which the shaft expands is determined by the thickness of the shaft wall, the relative thermal conductivities of the shaft, and the fuel. Placing the fuel outside of the rod causes the initial shaft heating to be adiabatic and gives the maximum rate of expansion. Both types of control shafts are shown in figure 11. An externally fueled hollow shaft may be more desirable than the solid shaft because of hollow shaft's higher structural strength and rigidity, which may be required to prevent bowing in the shaft. This in turn could lead to a thinner walled shaft and faster expansion rate.

A complete and accurate study of this concept would require three-dimensional heat-transfer calculations; however, as a feasibility study of the concept, only one-dimensional calculations were deemed necessary. The heat-transfer model is based on an azimuthal slice through the reactor and control shaft just wide enough to catch all of the fuel in the shaft (as diagrammed in fig. 12). Two situations are represented: the internally fueled hollow shaft and the externally fueled solid shaft; both contain the same inventory of materials. The reflector is correctly modeled in the cylindrical form, but the regions about the shaft are represented by slabs 1-centimeter wide to stress the outward flow of heat. As long as the heat flow in the r direction is everywhere

nonnegative, the set of equations governing the uninsulated internally fueled hollow shaft are (see fig. 12(a)).

$$T_e = T_f + \frac{I^2 Q \ln \frac{R}{I}}{2K_r} \tag{10}$$

$$T_f^4 = T_a^4 + \frac{I^2 Q}{2\epsilon\sigma} \tag{11}$$

$$T_a = T_b + \frac{I^2 RSQ}{2K_s(R + D)^2} \tag{12}$$

$$T_b = T_c + \frac{Q_f F^2}{2K_f} + \frac{RI^2 FQ}{2K_s(R + D)^2} \tag{13}$$

$$T_c = T_d + \frac{RI^2 SQ}{2K_s(R + D)^2} + \frac{FSQ}{K_s} \tag{14}$$

$$T_d = \left[\left(\frac{1}{\epsilon\sigma}\,\frac{RI^2 Q}{2(R + D)^2} + FQ_f\right)\right]^{0.25} \tag{15}$$

The average temperature in the fuel section is

$$\overline{T} = \frac{Q_f F^2}{12K_f} + \frac{T_b + T_c}{2} \tag{16}$$

In these equations

Q volumetric heating rate in core which flows towards shaft

Q_f volumetric heating rate in shaft fuel

S wall thickness of shaft

F thickness of fuel section in shaft

I radius of core-reflector interface

R radius of reflector outer surface

D distance between reflector and shaft fuel

K_r thermal conductivity in reflector

K_s thermal conductivity in shaft

K_f thermal conductivity in fuel

T_i temperature at point i as indicated in fig. 13(a)

ϵ grayness factor

σ Stefan-Boltzmann constant

As stated before two main types of shafts were investigated: an internally fueled hollow shaft and an externally fueled solid shaft. Parallel calculations were made for the reference ^{235}U and 233U reactors. The boundary temperatures were 1800 K at the core-reflector interface (the operating temperature of the heat pipes) and 0 K for no return radiation on the sink side of the model. Both shafts were studied in the insulated and uninsulated state, and the shafts were placed at several locations between 5 and 40 centimeters from the reflector. Finally, the reactor operating power was varied over a range from about 0.1 to 2.0 megawatts to get data points on both sides of the nominal 1-megawatt operating point.

Temperature variations across the shaft are small at all positions of the shaft (see in fig. 13). Whether insulated or not, a tantalum shaft 10 centimeters from ^{235}U reactor at 1 megawatt has a 10 K difference; 10 centimeters from a ^{233}U reactor, a 17 K difference. Tungsten and molybdenum shafts have about a 1 K smaller difference. The temperature distributions for a tantalum shaft are shown in figure 13 for the reactor at the 1-megawatt power level. The temperature drop across the reflector is less 1^{o}. If the fueled shaft were not present the temperature drop across the reflector would be about 95 K. In this case the inner surface of the reflector (at about 1700 K) would be radiating heat space. If the entire radial surface of the reactor could radiate to space, the reactor would lose 4.5 percent of its power through radiation. This is true for either a tungsten or a molybdenum reflector since these materials have almost identical thermal conductivities. As the power level changes, the temperature of the shaft changes in direct proportion because of the fuel in the shaft (fig. 14). At 1 megawatt the ^{235}U reactor is producing an average of 48 watts per cubic centimeter of the core, or 95 watts per cubic centimeter in the UC fuel; in the ^{233}U reactor at the design power level, the production rate is 212 watts per cubic centimeter in the core

and 420 watts per cubic centimeter in the UC. The fuel in a shaft 10 centimeters from the reflector is producing 5.55 watts per cubic centimeter in the ^{235}U case and 9.36 watts per cubic centimeter in the ^{233}U case. These production rates all remain in the same proportion and vary directly as the reactor power. As shown in figure 15 the heating rate in the shaft attributable to radiation from the reactor falls off sharply above normal 1 megawatt operating level and levels out below 1 megawatt. At the 1 megawatt power level radiative heating supplies 0.103 watt per cubic centimeter (1.82 percent of the total heating) for the ^{235}U reactor and 0.0522 watts per cubic centimeter (0.56 percent of the total heating) for ^{233}U. These figures are based on the radial component of the neutron current emanating from the radial reflector outer surface at the midplane with no gap present between the core halves. This current is 7.97×10^{12} neutrons per square centimeter per second for the ^{235}U reactor and 1.02×10^{13} neutrons per square centimeter per second for the ^{233}U reactor. The current averaged over a 10-centimeter height centered on the midplane is 0.942 of the midplane current for the ^{235}U reactor; over a 20-centimeter height likewise centered it is 0.840 of the midplane current. A compensating factor for this difference is the streaming of neutrons from the reactor operating with a gap. For these calculations the compensation is assumed equal to the averaging, so that the peak current is used in determining the shaft fuel heating effect.

The coefficient of linear expansion α is defined by

$$\alpha = \frac{\frac{\Delta L}{L}}{\Delta T} \tag{17}$$

where L and T are the length and temperature of the specimen. The temperature reactivity coefficient $\Delta\rho/\Delta T$ is governed by the relation

$$\frac{\Delta \rho}{\Delta T} = \frac{\Delta \rho}{\Delta L} \frac{\Delta L}{\Delta T} = \alpha L \frac{\Delta \rho}{\Delta L} \tag{18}$$

where ρ is reactivity. In the ^{235}U reactor $\Delta\rho/\Delta L$ is 3.0 percent δ k/k per centimeter, and in the ^{233}U reactor is 2.4 percent δ k/k per centimeter, where k is the effective multiplication factor. The power reactivity coefficient $\Delta\rho/\Delta Q$ is the product of the temperature reactivity coefficient and rate of change of temperature with respect to power; it is found from the equation

$$\frac{\Delta \rho}{\Delta Q} = \frac{\Delta \rho}{\Delta T} \frac{\Delta T}{\Delta Q} = \alpha L \frac{\Delta \rho}{\Delta L} \frac{\Delta T}{\Delta Q} \tag{19}$$

where Q is the reactor thermal power. In equation (19) α and L refer to the shaft

and $\Delta T/\Delta Q$ is found in the solution of the steady-state heat-transfer equations for different power levels. Taking the average temperature $\overline{T}$ across the fuel region as the average across the shaft is an approximation for the internally fueled shaft since the temperature drop across the shaft wall is never more than 2 or 3 K. In the calculations for the externally fueled solid shaft, the material positions were interchanged, so that the $\overline{T}$ in the case is the actual calculated average shaft temperature.

Results from these calculations are presented in figures 16 to 19. Figures 16 and 17 give the power reactivity coefficient against reactor power level. Since the reactor is assumed to be just critical at the 1 megawatt full power level, the power reactivity coefficient may also be given by

$$\frac{\Delta \rho}{\Delta Q} = \frac{\rho}{Q - 1} \tag{20}$$

for Q in megawatts. In figure 11 the power coefficients are plotted for 10-centimeter refractory metal shafts 10 centimeters from the ^{233}U and ^{235}U reactors. Being smaller, the ^{233}U has a higher neutron leakage rate and a current less spread out 10 centimeters away from the reflector. Couple this with the more sensitive gap worth of the ^{233}U reactor, and one finds the ^{233}U reactor with power reactivity coefficients about four times those in the ^{235}U reactor; for a comparison of this see the listing of coefficients in table II. The effect of insulating the shaft with 2-centimeter-thick beryllium oxide is worth 11.35 cents per megawatt for the ^{233}U reactor and 2.15 cents per megawatt for the ^{235}U reactor with a 10-centimeter tantalum shaft located 10 centimeters from the reflector (fig. 17). Figures 18 and 19 give the power reactivity coefficients against distances. They show that this coefficient varies very nearly with the inverse of the radius from the center of the reactor to the shaft. The slight curvature, which deviates from a straight-line inverse proportionality, is attributed to the temperature variation of the coefficients of linear expansion. The effect of insulation and distance combined is shown in figure 18. Without the insulator the proportionality is less than the inverse of the radius.

These results pertain to a 10-centimeter-long shaft. If the streaming current just compensates for the average peak ratio for the 10-centimeter shaft, then a 20-centimeter-long shaft is 1.78 times as effective as a 10-centimeter shaft of the same cross section. In this case a 20-centimeter insulated tantalum shaft 10 centimeters from the ^{235}U reactor is worth 22.23 cents per megawatts compared with 12.49 cents per megawatts for a 10-centimeter shaft.

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TABLE I. - DELAYED NEUTRON FRACTIONS FOR FISSIONS FROM FAST NEUTRONS

[From ref. 12]

Fission nuclide	Delayed neutron fraction, β_{eff}
^{235}U	0.0064
^{233}U	.0026
^{239}Pu	.0020

TABLE II. - POWER REACTIVITY COEFFICIENTS FROM FUELED CONTROL SHAFTS IN SPLIT-CORE REACTORS

[Thermal power level, 1 MW; shaft length, 10 cm; shaft insulation, beryllium oxide; distance between shaft and reflector, 10 cm.]

Shaft material	Power coefficient, $\Delta\rho/\Delta q$, -¢/MW	
	Uranium-233[a] reactor	Uranium-235 reactor
Molybdenum	51 (38)	13 (10)
Tantalum	48 (37)	12 (10)
Tungsten	32 (26)	9 (7)

[a]Numbers in parenthesis are for uninsulated shaft.

TABLE III. - THERMAL PROPERTIES OF MOLYBDENUM, TANTALUM, TUNGSTEN, AND URANIUM CARBIDE

Material	Thermal conductivity, K		Heat Capacity, C_p		Coefficient of linear expansion, α, 10^{-6}(cm/cm)K, at-	
	J/sec·cm·K	cal/sec·cm·K	J/g·K	cal/g·K	1250 K	1750 K
Molybdenum	1.09	0.26	0.38	0.091	7.6	9.3
Tantalum	.79	.19	.16	.038	7.7	8.8
Tungsten	1.05	.25	.19	.045	5.6	6.6
Uranium Carbide	.20	.048	.27	.065	---	11.0

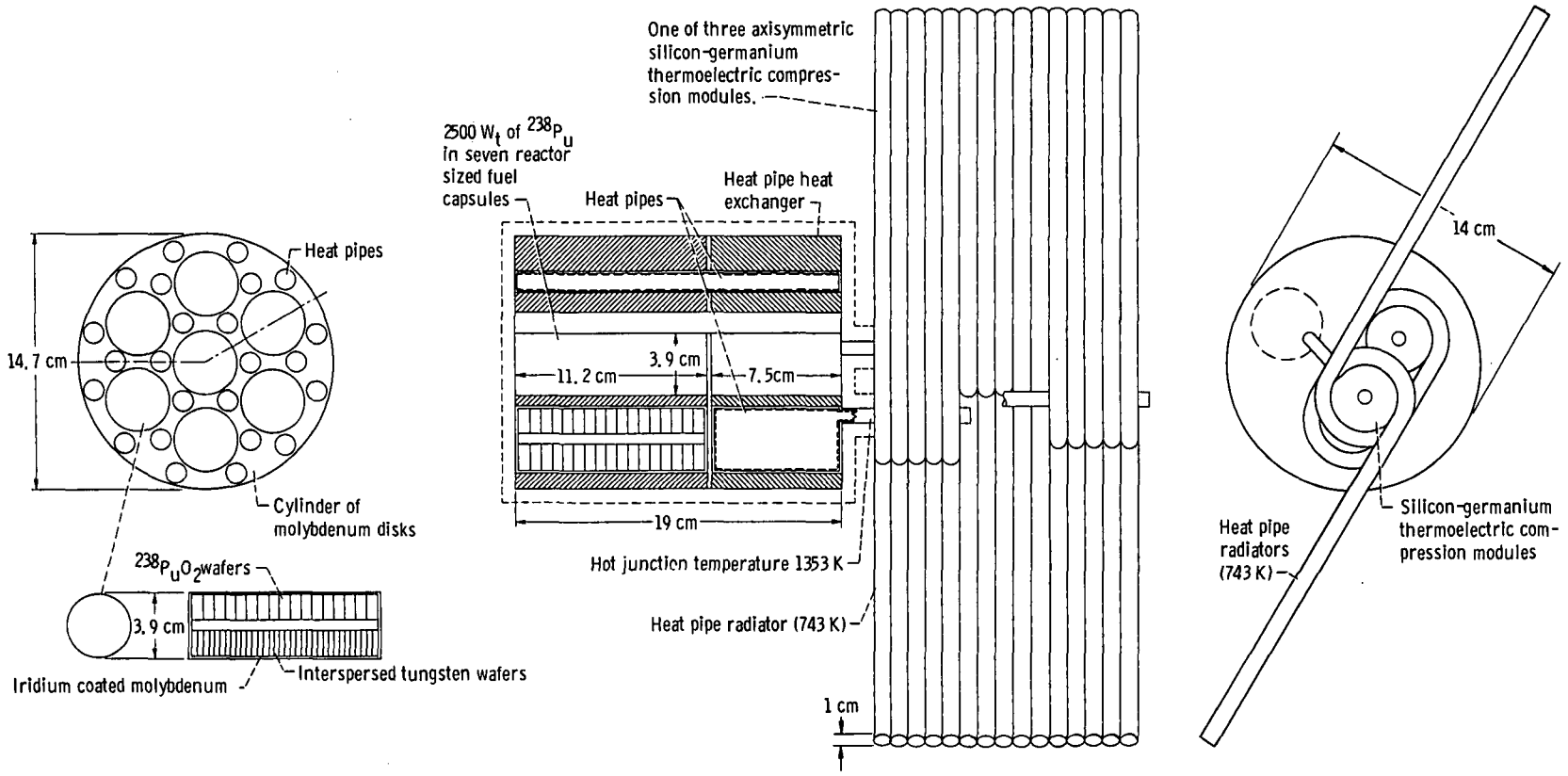


Figure 1. - 160 Watt plutonium 238 dioxide ($^{238}P_uO_2$) thermionic generator.

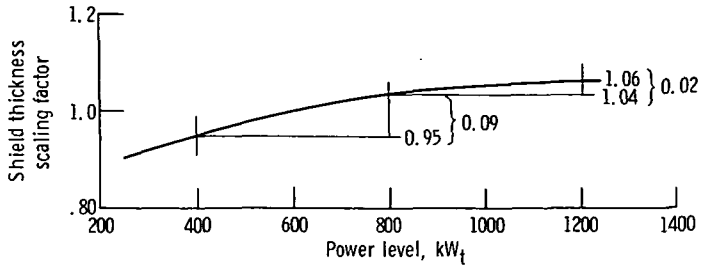


Figure 2. - SNAP-8 shield thickness scaling factor as a function of power level. Thickness increase for factor of 2 increase in power (400 to 800 kW), 0.09/0.95 = 9.5 percent; thickness increase for factor of 4 increase in power (400 to 1200 kW), 0.11/0.95 = 12 percent. (Data from ref. 11.)

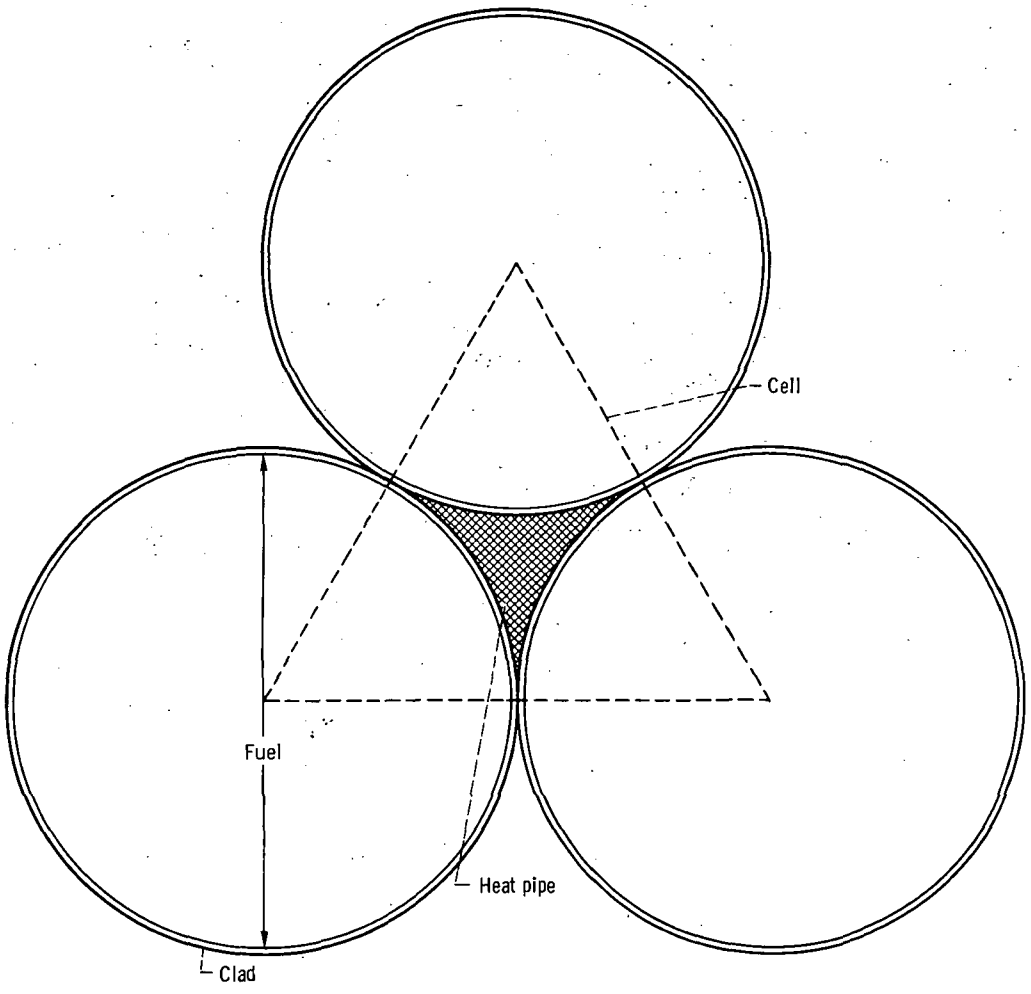


Figure 3. - Compact core cell

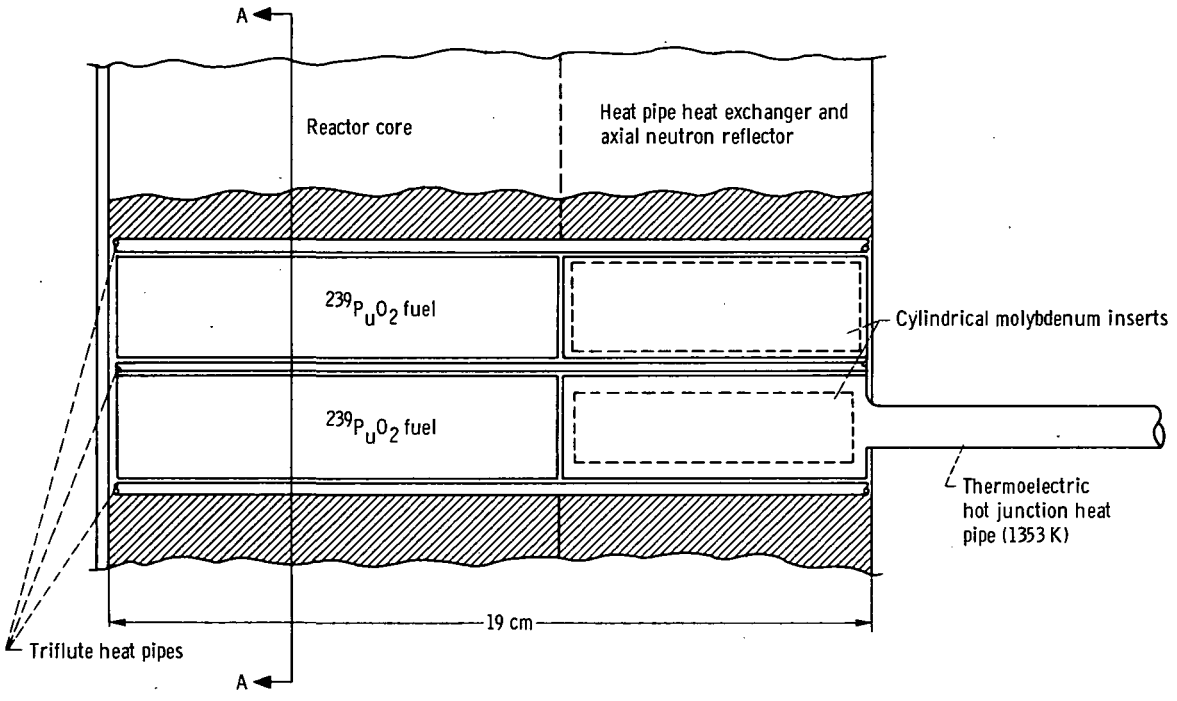


Figure 4. - Cutaway axial view of reactor configuration.

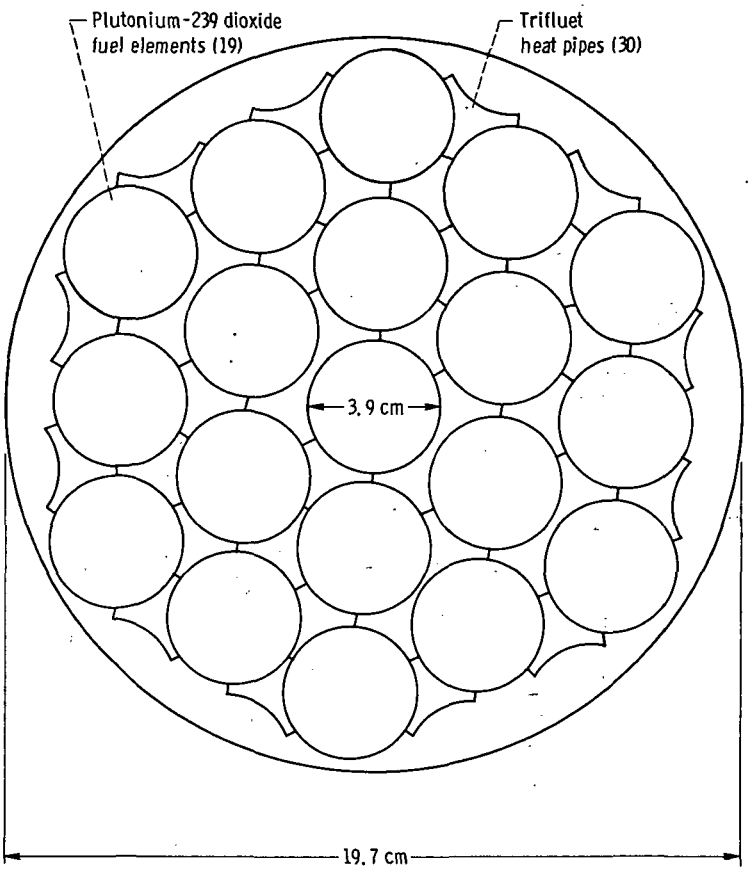


Figure 5. - Split-core 5-electrical kilowatt reactor configuration.
(Section A-A of fig. 4.)

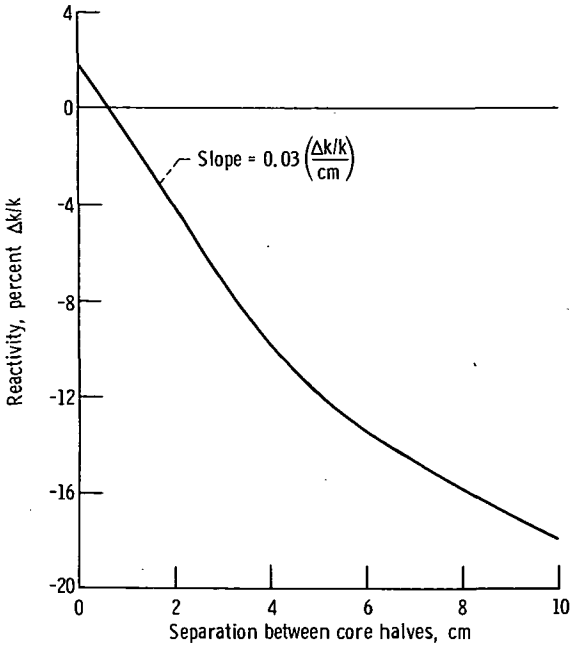


Figure 6. - Split-core reactor control curve (ref. 14). Fuel, uranium-233.

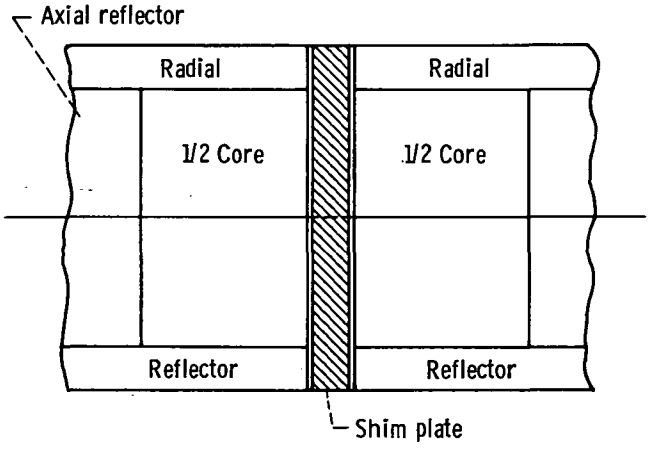
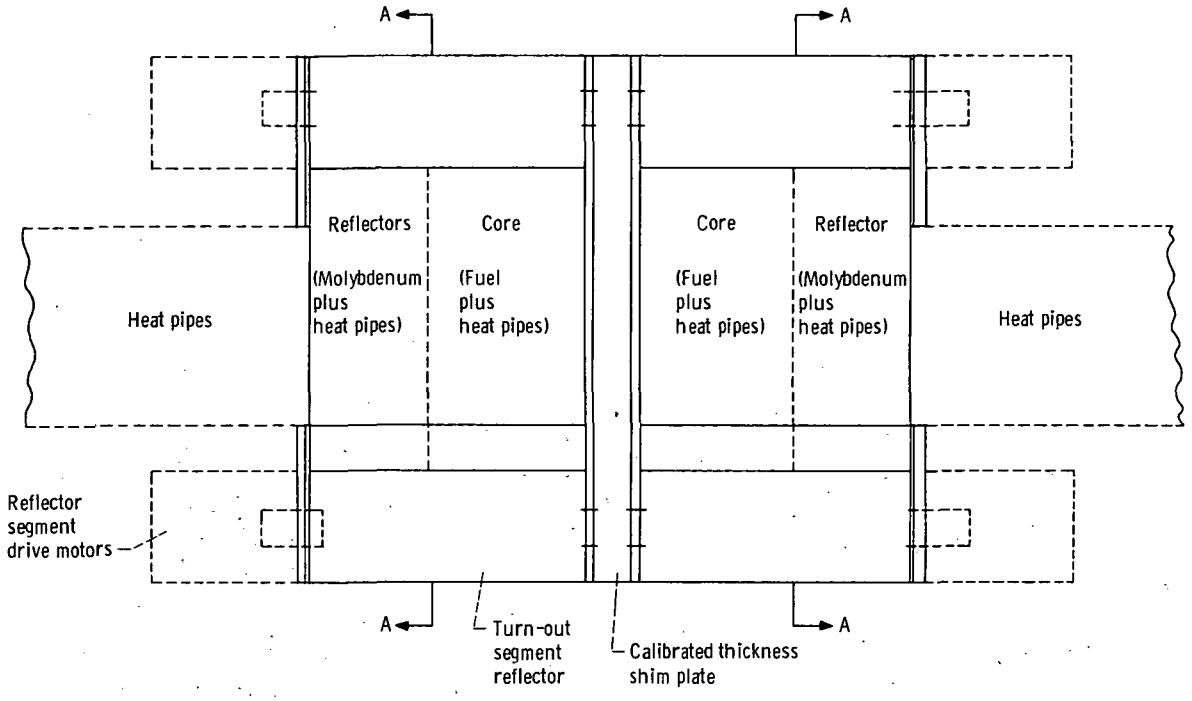
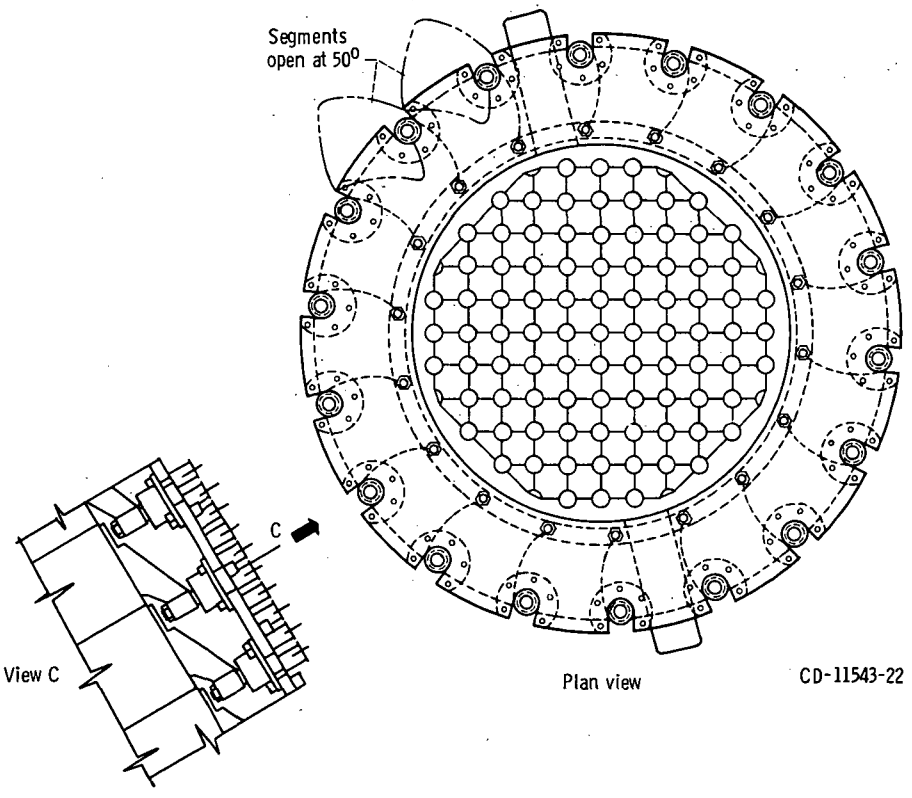


Figure 7. - Split-core reactor with shim plate to prevent fuel from moving toward central plane.



(a) Side view.



(b) Plan view.

Figure 8. - Split-core reactor with turn-out segment reflector control.

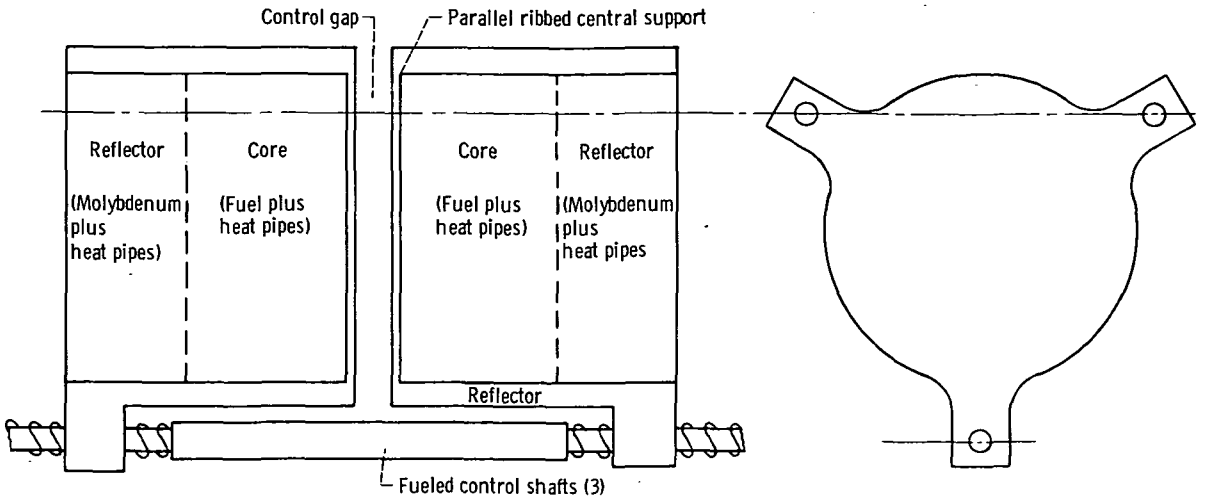


Figure 9. - Split-core reactor with fueled control shafts.

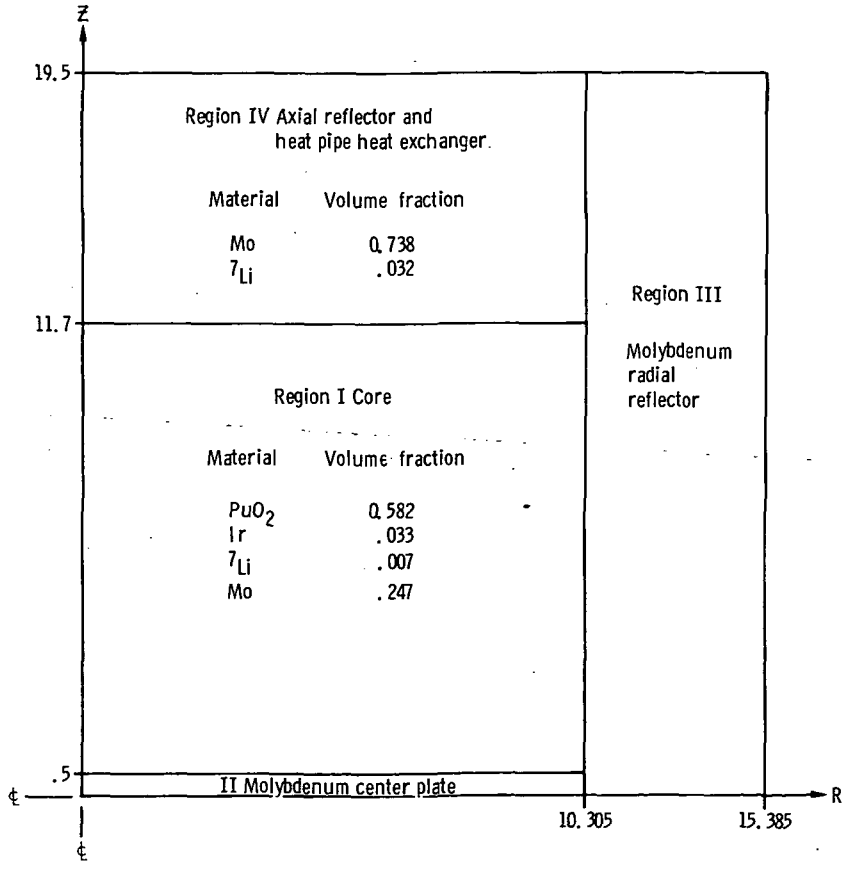


Figure 10. - R-Z model of reactor version. (Dimensions are in cm.)

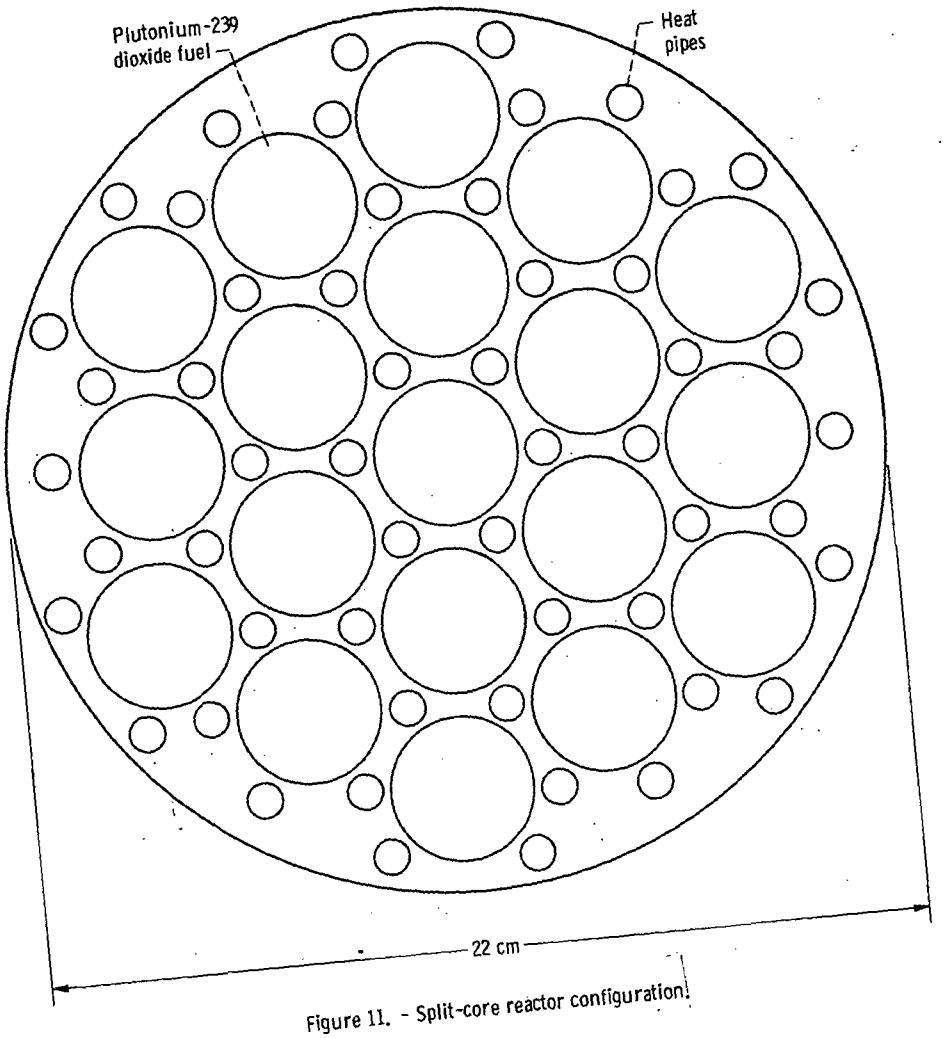
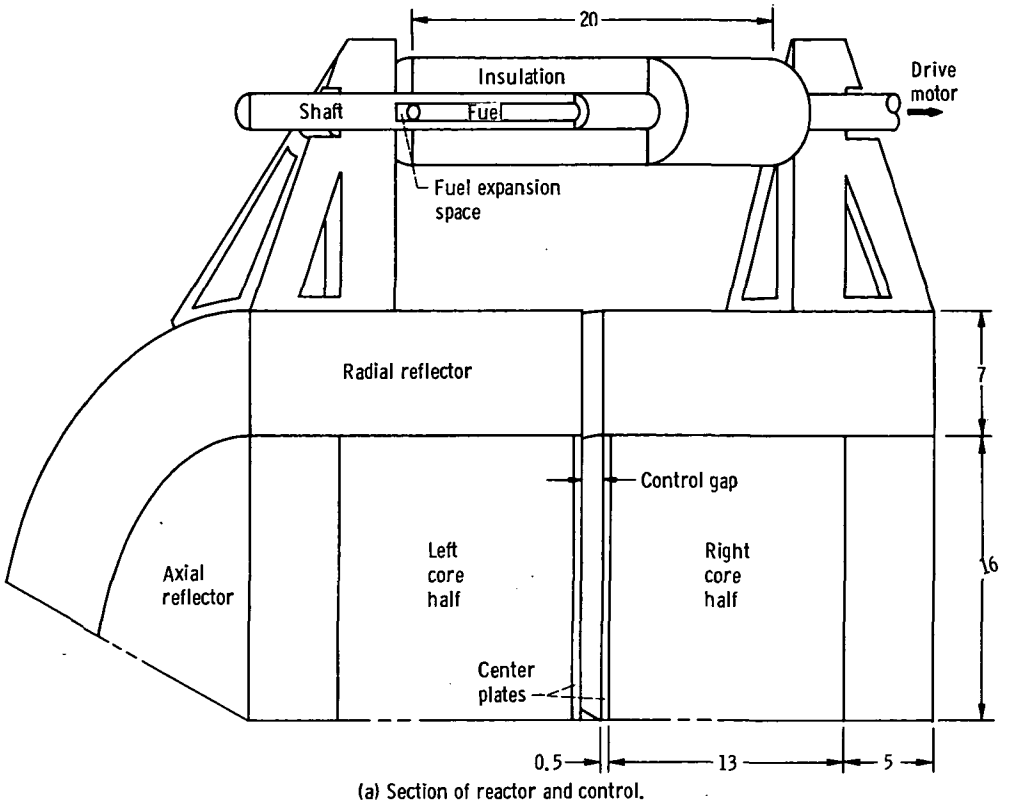
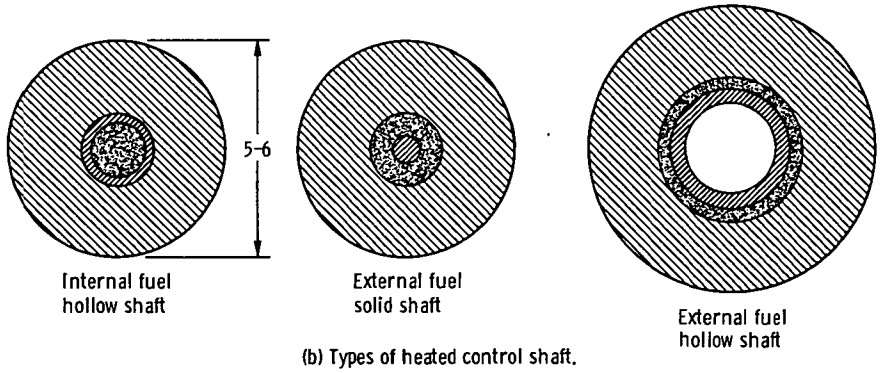


Figure 11. - Split-core reactor configuration.



(a) Section of reactor and control.



(b) Types of heated control shaft.

Figure 12. - Fueled control shaft concept.

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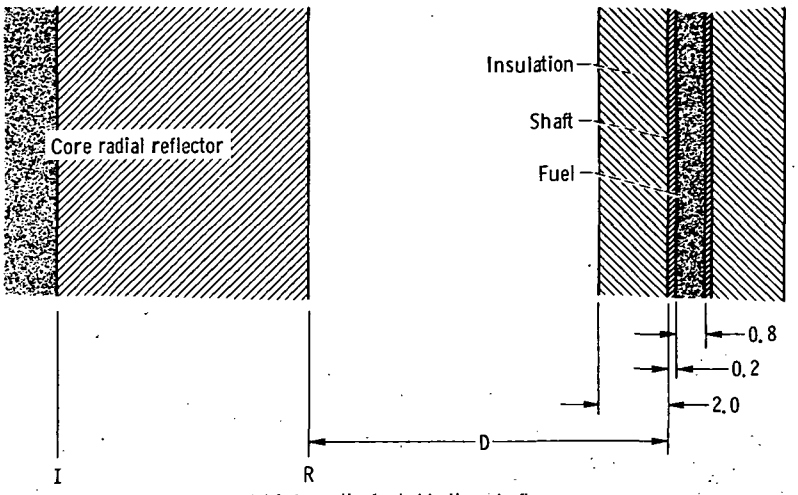
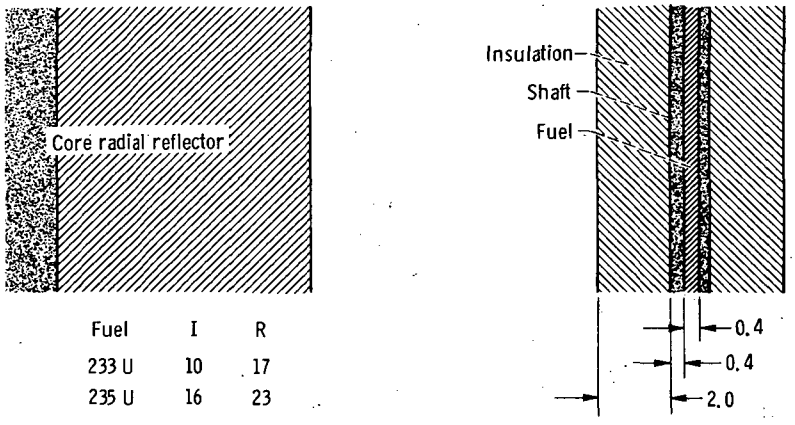


(a) Internally-fueled hollow shaft.



Fuel	I	R
233 U	10	17
235 U	16	23

(b) Externally-fueled solid shaft.

Figure 13. - One-dimensional models for fueled shaft heat-transfer study. (All dimensions are in cm.)

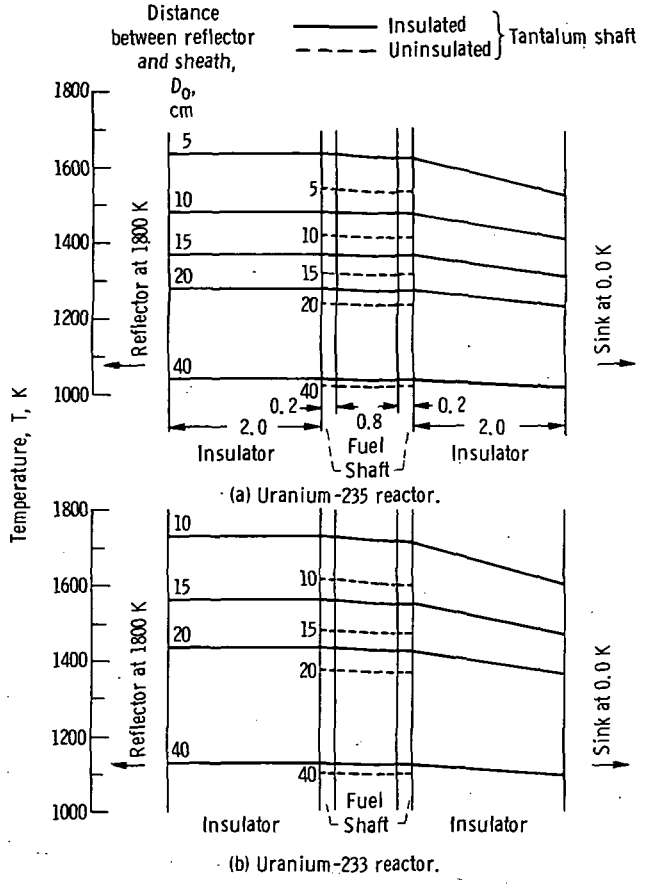


Figure 14. - Temperature distributions in control shafts.

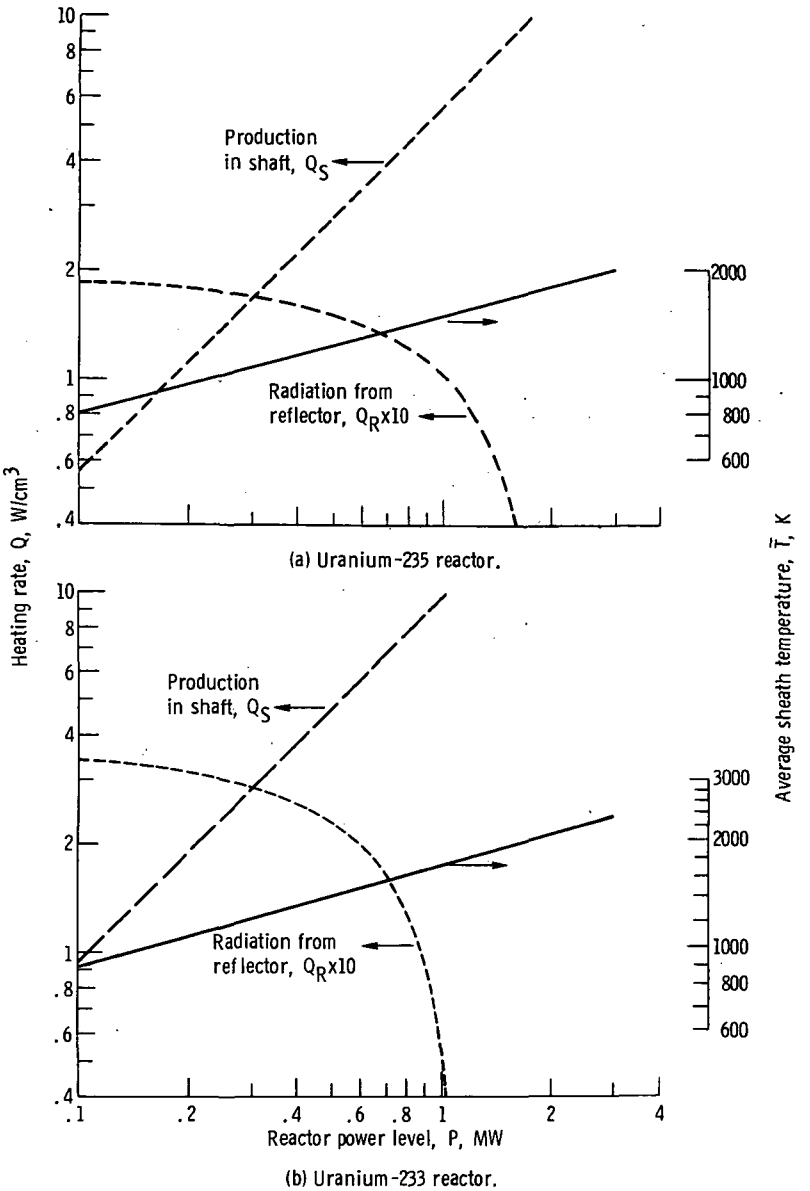


Figure 15. - Power sensitivity of heating rate and of temperature in tantalum shaft. Tantalum shaft; insulated; distance between reflector and sheath, 10 centimeters.

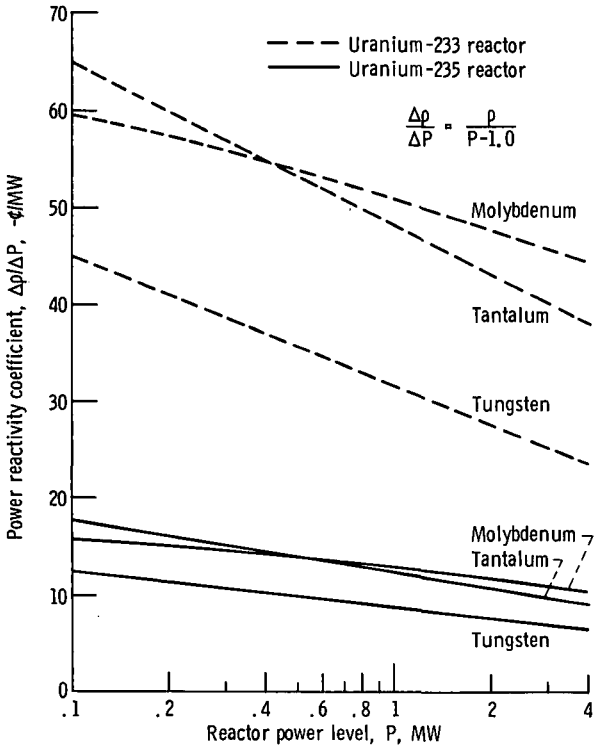


Figure 16. - Power level effect on power reactivity coefficients. Insulated shaft; shaft length, 10 centimeters; distance between reflector and sheath, 10 centimeters.

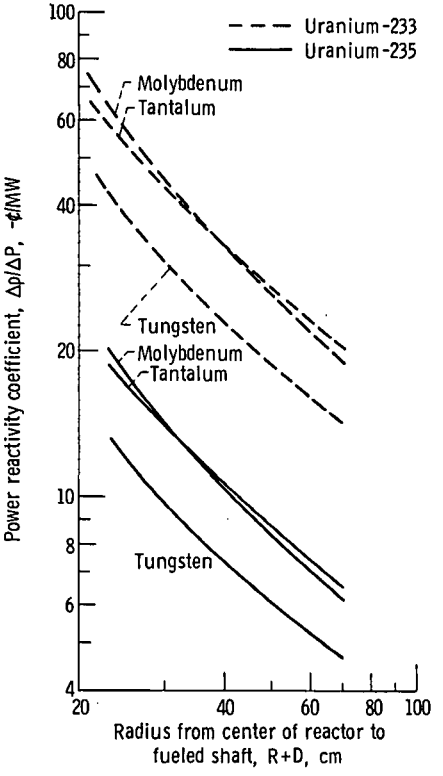


Figure 17. - Effect of distance on power reactivity coefficients. Insulated shaft length, 10 centimeters.

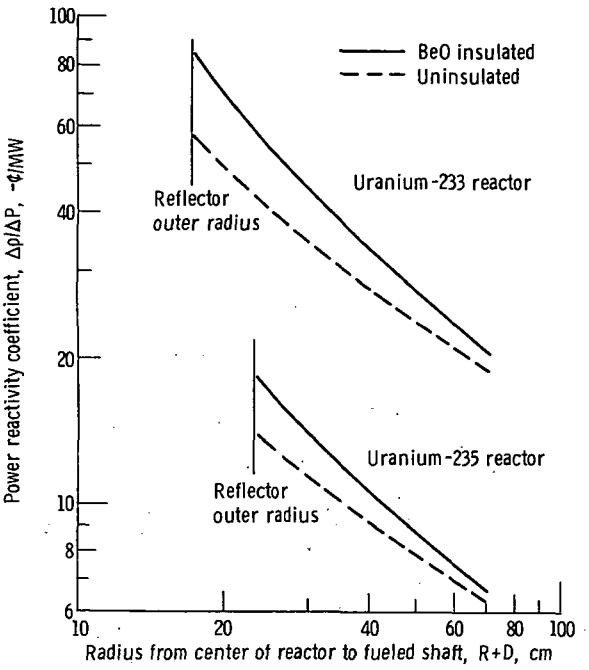


Figure 18. - Effect of insulation and distance on power reactivity coefficient. Tantalum shaft; shaft length, 10 centimeters.

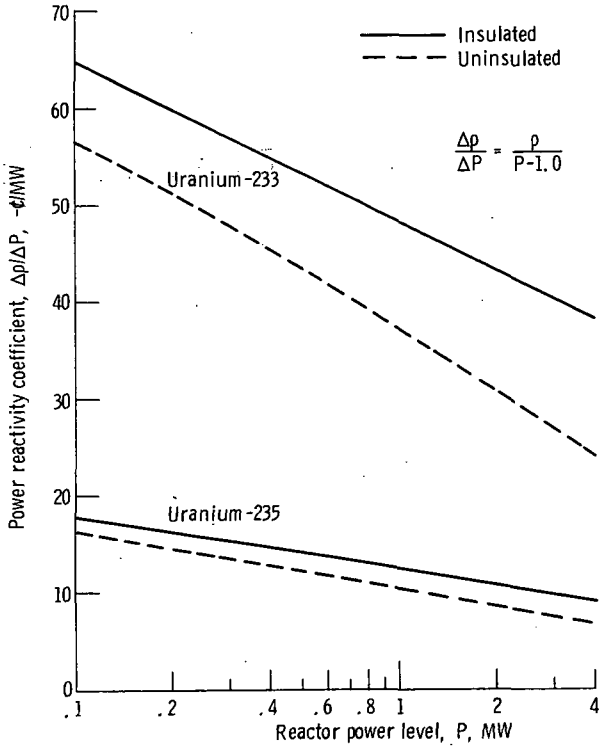


Figure 19. - Effect of insulation and power level on power reactivity coefficient.

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